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Damage Tolerance Assessment of Aging Nene X Turbine Discs

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ABSTRACT

The paper describes the application of the damage tolerance methodology to predict a safe inspection interval (SII) for the Nene-X engine turbine disc. The fracture critical location was established on the basis of stress analyses performed using 2D and 3D finite element models. To establish the stress intensity factor dependence on crack size in the fracture critical location of the disc, finite element based fracture mechanics analyses were conducted for both through-thickness and thumbnail cracks with different surface-length to depth ratios. Fatigue crack growth rate data were generated on compact tension (CT) specimens machined from a high time disc. Deterministic fracture mechanics (DFM) and probabilistic fracture mechanics (PFM) calculations were performed to compute a safe inspection interval for the disc.

1. INTRODUCTION

NATO countries are currently faced with the need to operate fleets of mature gas turbine engines. Because of diminishing resources for new equipment, the prospects of replacing these engines with new ones are not good at present. How long such engines can be kept in service safely, without replacing a significant portion of their aging structural components, has become a growing concern to engine life-cycle managers, due to uncertainties in residual lives. Another concern is the high maintenance cost associated with the replacement of durability-critical components, such as blades and vanes. The need to balance risk and escalating maintenance costs explains the growing interest in the application of life extension technologies for safely extracting maximum usage out of life-limited parts, [1], [2]. This paper describes the application of a damage tolerance approach to predict a safe inspection interval (SII) for the Nene-X engine turbine disc.

Nene-X engines power the Canadian Forces (CF) CT-133 aircraft. The original low cycle fatigue (LCF) life limits, also called the safe life (SL), for critical rotating components of Nene-X engines were calculated by the engine manufacturer, Rolls-Royce Ltd, Bristol, UK (RR) in the 1970s. At that time, RR had recommended a SL expressed in number of cycles, which when converted to flight hours, using a then specified number of LCF cycles per typical flight hour, yielded a 10,000 hours Nene-X turbine safe life. However, on the basis of the results of a more recent pilot survey conducted by the Canadian Forces (CF) and an analysis of this survey by RR, the factored exchange rates were revised. As a result of this revision, the safe lives of critical Nene-X components, expressed in hours, were reduced substantially. One of the most severely affected components of the engine was the turbine disc since approximately 25% of the discs in CF operation had exceeded the revised safe life limit. These parts needed to be replaced, however no spares were available. To maintain operations, it was decided to investigate the use of a damage tolerance based, safety by inspection, life cycle management scheme for this critical Nene-X engine components.

Paper presented at the RTO AVT Symposium on "Ageing Mechanisms and Control: Part B – Monitoring and Management of Gas Turbine Fleets for Extended Life and Reduced Costs", held in Manchester, UK, 8-11 October 2001, and published in RTO-MP-079(I).

To implement this approach, development work was needed to quantify the component damage tolerance in terms of fatigue crack growth rates, at the temperatures representative of the fracture critical locations of the disc, and to predict a fracture mechanics based safe inspection interval (SII). A key requirement of this analysis was the determination of the SII that would assure that an initially undetected crack growing in a component in service would be detected before it reached a critical length. This paper describes the SII prediction methods used for the Nene-X turbine disc and discusses the application of the damage tolerance based fleet management concept in the field.

2. DAMAGE TOLERANCE APPROACH

Under the safe life approach to life cycle management of turbine discs, the goal is to assure that only 1 in 1000 components likely develops a small fatigue crack at the end of a safe life period, Figure 1. The remaining 999 components clearly have some structural capability at the end of their safe design life. Continued safety of these components can be assured through a damage tolerance based safety by inspection life cycle management approach which relies on nondestructive inspection of components at overhaul.

Damage tolerance based life cycle management procedures assume that the fracture critical locations of a component contain cracks of a size just below the detection limit of the non-destructive inspection (NDI) technique used to inspect the discs at overhaul. The crack is then assumed to grow during service in a manner that can be predicted by linear elastic fracture mechanics or other acceptable methods, until a predetermined dysfunction limit is reached, beyond which the risk of failure due to rapid crack growth becomes excessive. The rates of crack growth and the dysfunction crack sizes are established analytically, based on the best estimates of service loads and material properties. The time or number of fatigue cycles required to grow the assumed crack (a_i) to its dysfunction size (a_d) is then used to define a Safe Inspection Interval (SII), usually by dividing the life to dysfunction by a safety factor. Probability of Detection information is also considered in the analysis.

This life cycle management concept is illustrated in Figure 2, which shows that at the end of one SII, all components are inspected and crack-free components are returned to service for another SII. This procedure can be repeated until a crack is found. In this manner the components are retired on an individual basis when their condition warrants such action. Usually deterministic fracture mechanics calculations are used to predict SII and probabilistic fracture mechanics methodologies are used to quantify risk. The US ENSIP damage tolerance approach uses some quantitative measures of the maximum crack size that may be missed during depot level inspection as the starting point for damage tolerance analysis.

Some assumptions have to be made in the damage tolerance assessment of old hardware due to a lack of availability of the detailed technical data. In the approach reported here it was assumed that

- cracks in the component are large enough to grow according to the Paris law,
- growth of the three-dimensional cracks is governed by the stress intensity factor at the deepest point,
- the effect of minor cycles can be neglected,
- only a one dimensional stress state is driving the crack,
- the temperature distribution in the disc can be inferred from the temperature values at the disc rim and the disc bore locations,
- the effect of compressive residual surface stresses can be neglected.

Conservative safety coefficients were used during the analysis to compensate for these simplifying assumptions.

3. STRESS ANALYSIS

Both two and three-dimensional (2D and 3D) finite element (FE) models of the disc were built. MSC.PATRAN and MSC.NASTRAN were used to perform modelling and numerical calculations. Thermal and mechanical loads were considered in the analysis. The disc operating temperature was determined from the data provided by Rolls-Royce Canada (RRC). The centrifugal force and fir-tree pressures were calculated using the Nene-X engine maximum operating rotational speed value.

A 2D simplified model of the turbine disc was analysed first. The results showed that the von Mises stress in the center of the disc was 53% of the UTS, a level which closely matched the data obtained from the engine manufacturer.

Ten different mesh patterns for FE analysis were generated in 3D to establish an optimum balance between numerical accuracy and available computational capacity. The 3D FE model used represented a disc segment of 13.33° angular section comprising two fir-trees with a fine mesh in one fir-tree and a coarser mesh in others as shown in Figure 3. Solid elements with 20 and 15 nodes were used to construct the model. The 3D disc model had 228,825 degrees of freedom (DOF).

Symmetrical boundary conditions were added at the isolated surface of the FE model. The blade centrifugal force was applied as an external load. The interaction of the blade and the disc was simulated as a uniform pressure applied to the lower surfaces of the fir-tree serration of the disc. The calculated pressure corresponded to the disc maximal rotational speed. In addition to mechanical loading, a thermal gradient was applied to the disc. This gradient was calculated on the basis of the boundary temperature values of 200°C at the disc center and 430°C at the rim using the heat transfer analysis capabilities of MSC.NASTRAN. Since the engine is primarily used in a cyclic mode, no hold time effects were considered in this study.

A submodelling approach [3] was used to increase the calculation accuracy in the critical area of the disc. In this approach, the global model used a 13.33° angular section that had already been analysed to identify an area with the highest stress level. This area was then isolated to build a local model for subsequent detailed analysis. The mesh pattern for this local model comprised one fir-tree only with a considerably refined mesh when compared to the global model. The displacements at the separation boundaries, the temperature distribution at the FE nodes as well as the pressures on the fir-tree serration derived from the global model were transferred to the local-model. To assess solution convergence, the model was further refined by increasing the number of finite elements in the disc thickness direction. This approach increased the numerical accuracy for the available computer resources.

The disc center and the bottom serration of the fir-tree were identified as two areas of high stresses in the 3D FE analyses. In the disc center, the highest von Mises stress reached 53% of the material UTS. In Figure 4, which shows the geometry of one fir-tree, Curves A and B are located at the root of the fir-tree and connect the front and back faces of the disc. Curves C and D are located at the center of fir-tree thickness along the circumferential direction. The highest von Mises stress is present at the intersection of curves B and D. Figures 5 and 6 provide details of the von Mises stress distributions at the highest stress location in the fracture critical area of the disc [4].

The 3D FE analysis confirmed that the root of the bottom fir-tree is the primary fracture critical location of the disc. These findings were further confirmed upon examining the engine operational logs where it was indicated that in the early 1970's, cracks had been found in the bottom serration region of Nene X disc.

4. FRACTURE MECHANICS ANALYSIS

The quantitative dependence of the stress intensity factor (SIF) on the crack depth is an important factor in the application of deterministic and probabilistic fracture mechanics (DFM/PFM) analyses for predicting a SII for the component.

Taking into account the length of the crack that can be missed during depot level inspection using the liquid penetrant inspection (LPI) technique, and utilising the results of a previous demonstration program performed on stainless steel compressor discs [5, 6], through-thickness and thumbnail cracks that were 4 mm and 8 mm in depth were studied. These crack depths corresponded to surface crack length in the range of 12 to 32 mm depending on the assumed semi-elliptical crack shape expressed by 2c/a ratio. In the analysis, the crack growth plane was assumed to be perpendicular to the lowest serration of the fir-tree as well as the disc radial line. This was based on previous operational experience with the engine and the stress analysis results that showed that the maximum von Mises stress direction at this fracture critical location lay in the radial direction.

In the FE based fracture mechanics modeling of the turbine disc, cracks were embedded at the fracture critical location. To properly model the stress field singularity in the crack vicinity, 3D singular elements were used around the crack front in the 3D finite element models. These elements were arranged by moving finite element middle nodes to the quarter point positions along element edges for wedge (PENTA) elements. The singular elements must be small enough to be fully included in the entire singularity dominated region in the vicinity of the crack front. Therefore, their sizes were carefully chosen to assure accuracy and convergence to obtain a finite element model that could be solved using available computer facilities.

The extraction of SIF from the finite element results can be done using field extrapolation near the crack tip or utilizing the energy release rate when a crack propagates. The methods based on stress field approximation require a finer mesh to produce a stress gradient around the crack tip, but recent findings [7] suggest that the displacement extrapolation technique can give very accurate predictions, even for a course mesh, if a good angular discretization is made around the crack tip region. This method was thus used for SIF recovery from the finite element results. The choice was mainly dictated by the features of the commercial software package (MSC.NASTRAN) that was available to the authors. The displacement approximation method uses nodal displacements, which are a primary output of the FE program. Three variations of the displacement field approximations in the SIF extraction process were used for comparison. They were: the method described by Shih *et al.* [8], the method presented by Chen and Kuang [9], and the method proposed by Chan *et al.* [10], which is the original formulation of the displacement approximation method. All three methods consider displacements at different nodes. A disadvantage of all three methods is that the user has to decide whether plane stress or plane strain conditions are operative ahead of the crack front. In the 3D FE calculations, plane strain conditions were assumed because of the large thickness of the disc in the fir-tree as compared to the crack sizes analysed. In addition, Newman *et al.* [11] have shown that, in the 3D case, the state of stress around the crack front is nearly plane strain, while the global deformation is accurately modelled by plane stress conditions.

For thumbnail cracks, surface crack length (2c) to crack depth (a) ratios of 3:1 and 4:1 were assumed. The aspect ratio 2c/a of 3:1 is often observed in aero engine discs, whereas a 2c/a ratio of 4:1 represents the worst case assumption. The relationships between the stress intensity factor K_I and the surface crack length (2c) for thumbnail surface cracks, embedded in the disc fracture critical location, with aspect ratios 2c/a of 4:1 and 3:1 are shown in Figure 7.

Using our past operational experience with gas turbine critical engines parts and the FE calculation results, an 8 mm deep thumbnail crack was selected as the dysfunction crack size for the disc. This is because the K_I value at the crack tip for a 2c/a ratio of 3:1 was approximately 45 $MPam^{1/2}$, which corresponds to 50% of the experimentally obtained fracture toughness (K_{IC}) value of 90 $MPam^{1/2}$ for the disc material.

5. FATIGUE CRACK GROWTH RATE

Upon reviewing the mission profile and the engine usage data, it was concluded that cyclic usage was the primary driver for crack initiation and growth processes in the Nene-X disc. Since the component is operating at a homologous temperature less than 0.4 under predominantly cyclic usage, the contribution of creep towards transgranular crack propagation was neglected. Therefore, fatigue crack growth rate (FCGR) data as a function of ΔK were generated on compact tension (CT) specimens machined from the high time disc and these data were used in the damage tolerance analyses. The turbine disc is made out of Rolls Royce modified Type 409 stainless steel. The high time disc was selected to account for any service-induced microstructural degradation effects on crack growth. The CT specimens, conforming to ASTM E-647 specifications, were machined from the material located close to the disc bottom rim serration. A CT specimen thickness of 13 mm was used to ensure that plane strain conditions prevailed ahead of the crack tip in all cases.

The CT specimens were pre-cracked in fatigue at room temperature to develop a sharp crack prior to cyclic loading using servo-controlled hydraulic test machines. All FCGR tests were performed in air at 430°C, which is representative of the disc rim temperature. Crack growth as a function of the number of cycles (N) was monitored using an automated DC-PD technique [12]. The crack length versus the number of cycles data, together with calibration curves, were used to plot FCGR as a function of ΔK.

The data show the existence of two crack growth regimes, the Paris regime at lower ΔK values up to 55 $MPa\sqrt{m}$, and the tertiary regime at higher ΔK values of the order of 55 to 65 $MPa\sqrt{m}$. The least squares method of linear regression was used to obtain the FCGR versus ΔK relationship in the Paris regime:

$$\frac{da}{dN} = C(\Delta K)^n \quad .$$

The parameters for the mean line was found to be C=1.29x10^{-7} mm/cycle, n=2.28 whith ΔK expressed in $MPa\sqrt{m}$. The conditional standard deviation was calculated to account for the scatter in the FCGR results. The parameter of the upper bound line of the scatter band was estimated as C=1.50x10^{-7} mm/cycle.

A power exponent of 2.28 in the Paris regime obtained from experiments is well within the typical range (2–4) observed for most stainless steels and superalloys under intragranular fracture conditions. The final crack lengths at specimen rupture were also measured at 430 °C to compute the fracture toughness for the material. The measured K_{IC} values were in the range of 87 to 95 $MPam^{1/2}$.

The fracture surfaces of all failed CT specimens were examined by scanning electron microscopy. All specimens revealed classical flat, occasionally striated, intragranular fracture characteristics. Such features are typically observed in most stainless steel materials.

6. DAMAGE-TOLERANCE BASED LIFE-CYCLE ANALYSIS

6.1 METHODOLOGY

Both, deterministic fracture mechanics (DFM) and probabilistic fracture mechanics (PFM) methodologies were used to predict the safe inspection interval for the Nene-X turbine disc. DFM methods are used to assess the damage tolerance of a part containing a crack at the detection limit of the inspection technique and using material data conforming to minimum assured material properties while PFM methods are used for assessment of the risks associated with various inspection strategies for engine component maintenance and life cycle management. The initial surface crack size used in DFM analyses corresponded to the 90% probability of detection (POD) with 95% confidence (90/95 POD) for the LPI techniques shown in Figure 8. A flow diagram of the DFM/PFM methodology is presented in Figure 9, while the basics and details of the both DFM and PFM methods are described elsewhere [5, 6].

Each set of simulated failure data obtained in PFM analyses was evaluated statistically. Two or three parameter Weibull, lognormal or gamma distributions were used as probability distribution functions (PDF) candidates which fit the simulated data [13]. Probability-plot-correlation-coefficient plots were used to initially estimate shape parameters for Weibull or gamma distributions. This determined which specified distribution family provides the best fit to the simulated disc failure data. Next, the three distributions were fitted to the data using both, the least square and the maximum likelihood methods. Finally, the Anderson-Darling goodness of fit test was performed for fitted PDF curves. This test is a modification of the Kolmogorov-Smirnov (K-S) test which gives more weight to the tails than the K-S test [14]. Sensitivity analysis was also performed to find the effect of the various input parameters on the calculated SII.

In-house PFM and DFM software packages were used to perform disc failure simulations, while MATLAB®, DATAPLOT [15] and in-house statistical analysis software were used to analyse the simulation results.

6.2. SAFE INSPECTION INTERVAL (SII) PREDICTIONS

The DFM calculation was performed to compute the surface crack length versus the number of cycles curves for the Nene X turbine disc [16]. A typical crack propagation curve for the disc, covering a surface crack length range of 6.0 mm to 32.0 mm, as a function of the number of cycles, is shown in Figure 10. The effect of the sensitivity and reliability of an NDI technique on SII predictions is clearly demonstrated in this figure because the crack propagation interval (CPI) value changes significantly with the initial surface crack length. For example, for a surface crack length of 6 mm, the CPI is 22,300 cycles while the CPI reduces to approximately 8,300 cycles for the initial crack length value of 16 mm.

A scatter factor (safety factor) must be defined. This safety factor is based on confidence in the analysis, material property data, usage and inspection capability. Typically this safety factor is between 2 and 5 based on representative testing, hence the SII would be between 4,400 and 11,000 cycles.

PFM analysis was used to simulate the consequences of missing a crack during inspection. The PFM approach uses the same linear elastic fracture mechanics (LEFM) principles for calculating a SII but input parameters such as initial crack size, crack size dysfunction, constants of Paris equation are treated as random variables. A range of SII was calculated and appropriate values were selected to maintain a sufficiently low but cost-effective probability of failure. The distribution of surface crack size missed was used randomly as the starting condition for each simulation. PFM simulation using 5000 point and assuming using the worst case scatter in the FCGR data, was carried out. In the case analysed, it was found that a log-normal distribution fits the simulated data better than either a Weibull or a gamma distribution. Typical cumulative probability distribution function (F) of cycles to dysfunction is presented in Figure 11 where normal deviate refers to the standard deviations of underlying normal probability distribution function. The 0.1%F which yields the number of cycles for which 1 in 1000 probability exists of a crack reaching dysfunction size were extracted from such plots. For the assumed scatter condition in the FCGR data, it was found that 0.1%F corresponds to 26,000 cycles.

Applying a safety factor of 2, the SII value of 13,000 cycles was calculated. Furthermore, assuming a usage of 5 cycles/hour for the engine, a SII of 2,600 engine operating hours is predicted.

7. EXPERIMENTAL VERIFICATION

Predicted SII are verified experimentally under simulated service conditions using a spin rig facility installed at the Institute for Aerospace Research of NRC. A damage tolerance test is being performed with this facility. For this test an artificial notch of 4 mm surface length and 1 mm deep was embedded in the Nene X turbine disc lowest firtree serration using the EDM technique. The disc was thoroughly inspected prior to the test using LPI and eddy current techniques. A custom made eddy current probe was used for inspections of the disc firtree serrations. No flaws were found in the disc during these initial inspections. The disc containing the artificial notch and undergoing spin testing is removed from the spin rig facility periodically and inspected using visual and eddy current based techniques. At the time of writing of this paper the disc has accumulated approximately 3000 cycles without any signs of a propagation of the embedded flaw. Spin rig testing of the disc is continuing.

8. CONCLUSIONS

The bottom serration region was determined to be the fracture critical location in the Nene-X turbine disc. The dysfunction surface crack length crack for the disc is estimated to be 32 mm using a range of assumptions for the 2c/a ratios in a crack of a semi-elliptical crack shape. This dysfunction crack size value was based on experimentally determined fracture toughness values of the rim material at an operating temperature of 430 °C. The SII was computed on the basis of FCGR data alone, and no creep effects were considered in the analysis.

PFM calculations revealed that if the scatter in material properties is represented by the experimental scatter in FCGR data, a 1 in 1000 chance exists for a crack to reach the dysfunction size in 26,000 cycles, if the LPI technique alone is used to inspect the discs.

DFM calculations showed that the crack propagation interval for the Nene-X disc is of the order of 22,000 cycles.

The results presented in this paper, along with other data, permitted the CF fleet managers to make a decision regarding life cycle management of the Nene X engine fleet. Significant saving can be achieved by using the damage tolerance approach, because discs can be used beyond their OEM assigned safe life limit.

ACKNOWLEDGEMENTS

The project performed at the National Research Council Canada, Institute of Aerospace Research was funded by Department of National Deference (DND). The authors are grateful to DND's Scientific Authority for permission to publish the work. The authors acknowledge the finite element calculations performed by Dr. S.M. Cheng, the crack growth testing done by Ms. R. He and also contributions made by Dr. X. J. Wu.

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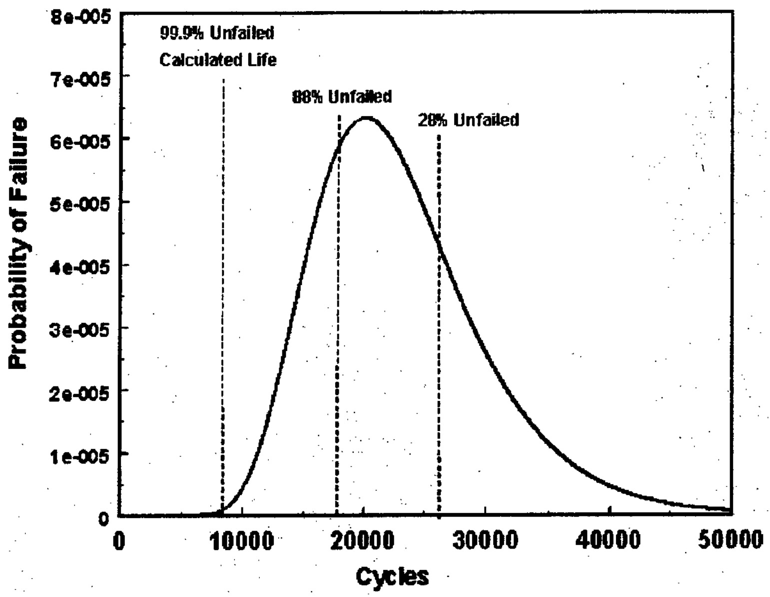

Figure 1. Probability of failure in the safe life approach.

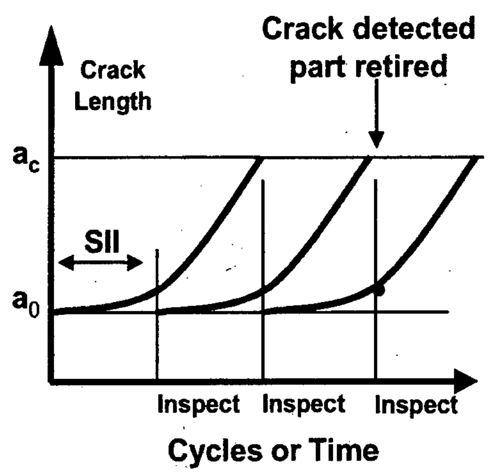

Figure 2. Schematic representation of the damage tolerance based life cycle management approach.

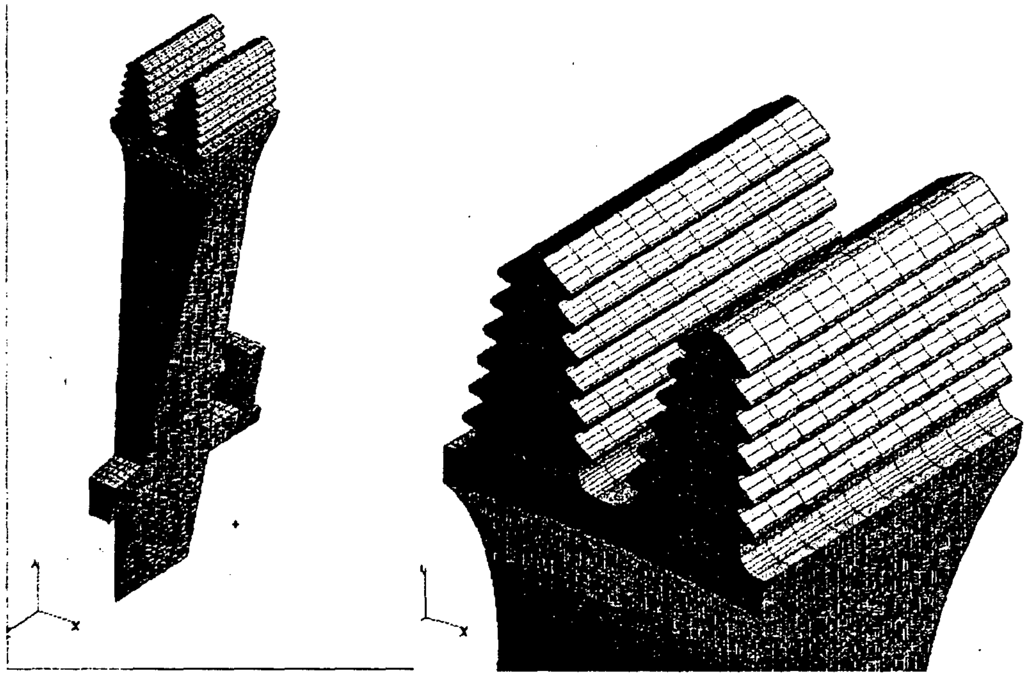

Figure 3 FE model for the turbine disc

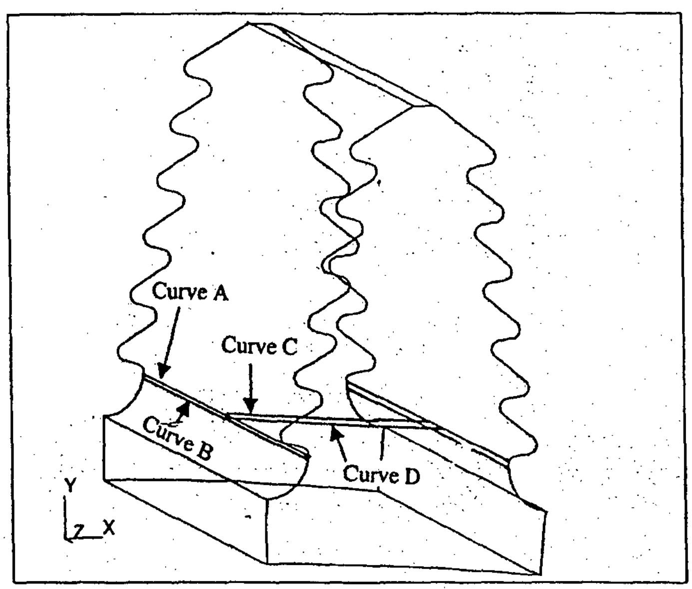

Figure 4. Geometrical configuration of the fir-tree.

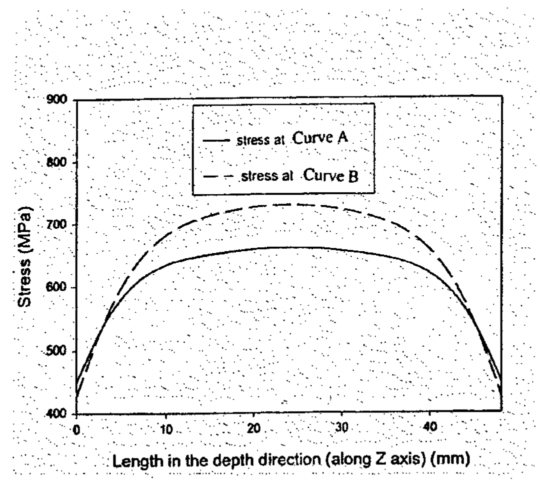

Figure 5. Von Mises stress distribution in the disc thickness direction below the innermost serration of the disc. This is the highest stress region.

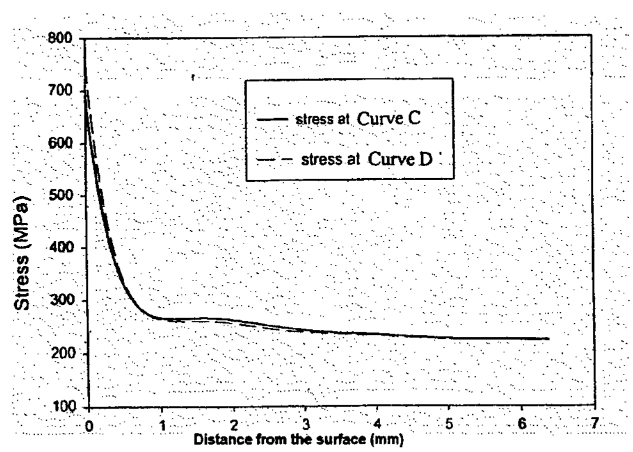

Figure 6. Von Mises stress distribution in the depth of the material starting from the centre of the lowest serration.

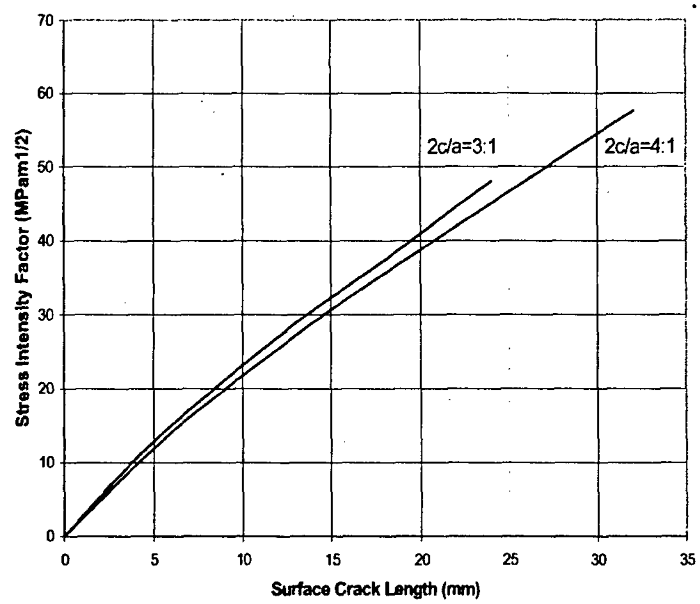

Figure 7. Stress intensity factors for two semi-elliptical cracks embedded in the lowest serration of the firtree.

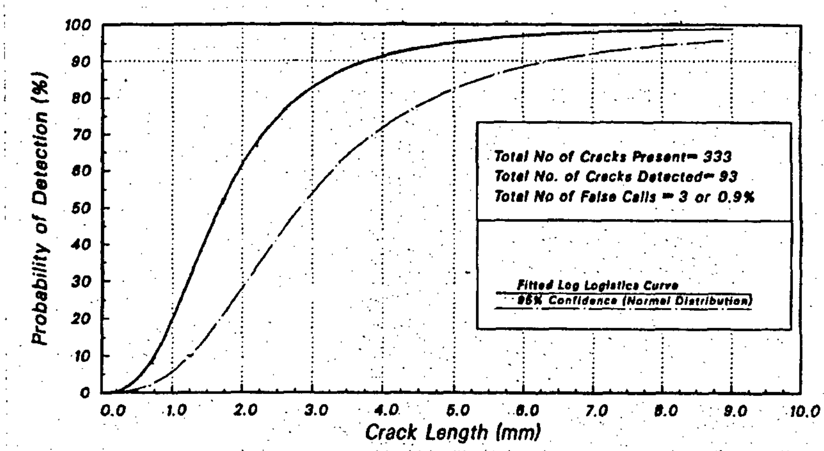

Figure 8. POD for natural cracks in Fe-Ni-Cr alloy turbine disc using the LPI technique 9 (after Koul et al. [5], [6]).

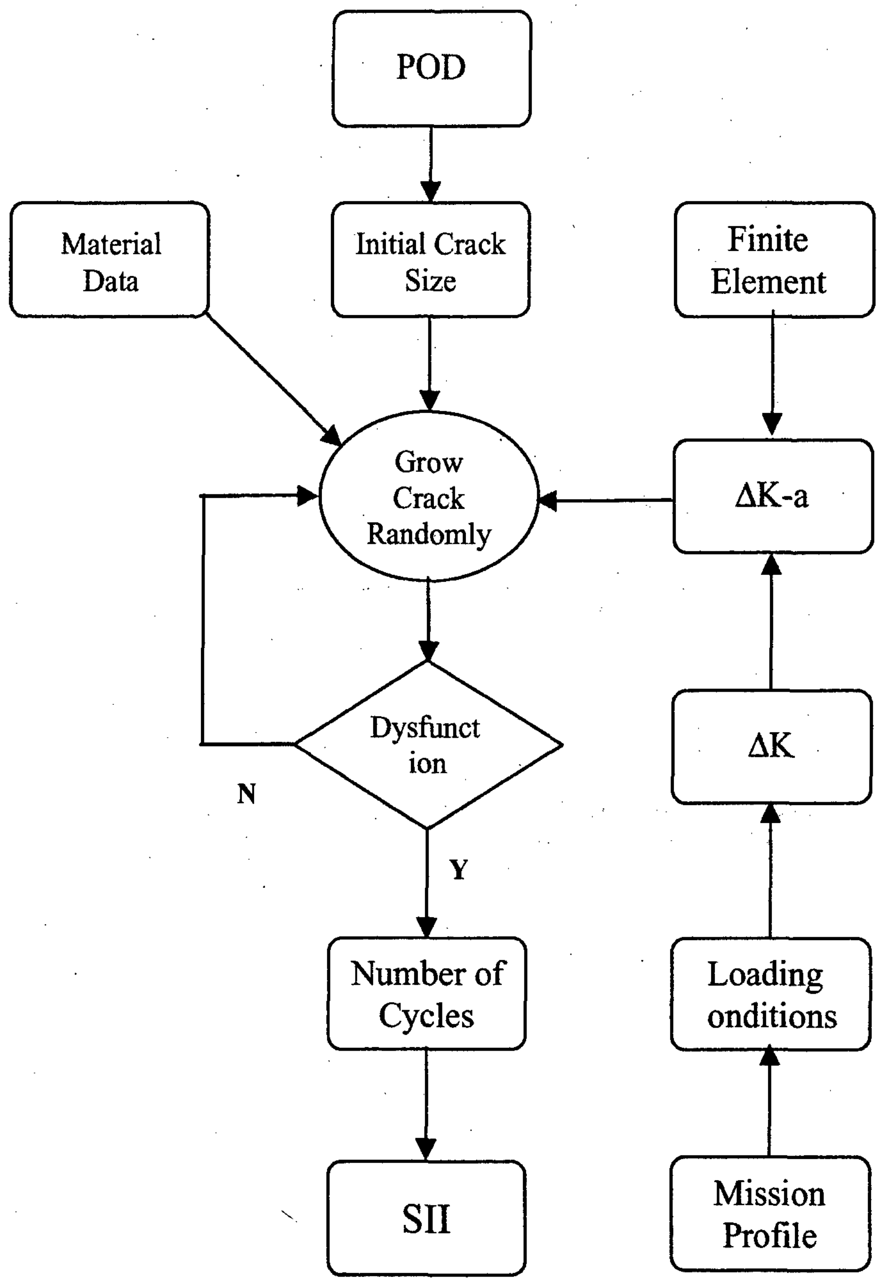

Figure 9 A flow diagram of the DFM and PFM program used to calculate SII for the Nene-X disc.

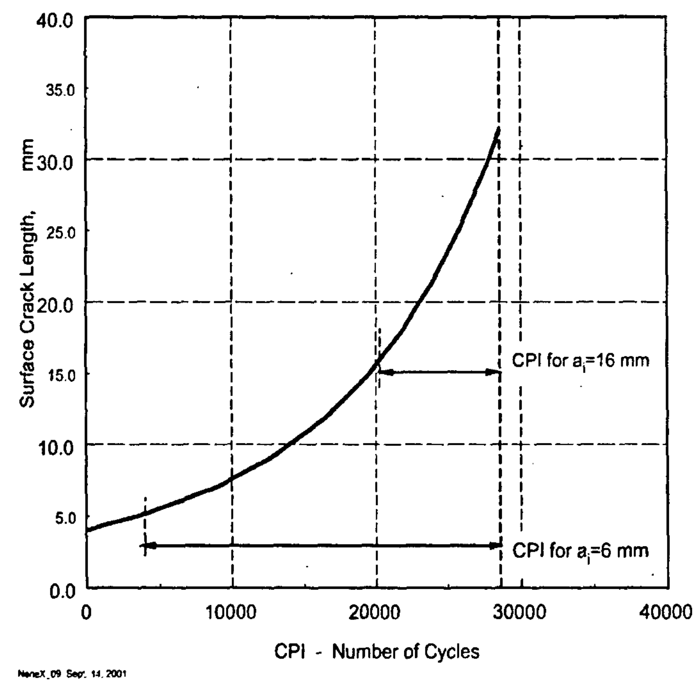

Figure 10. Damage tolerance based life cycle management curve for Nene-X turbine discs using 90/95 POD value of the LPI technique as the detection limit.

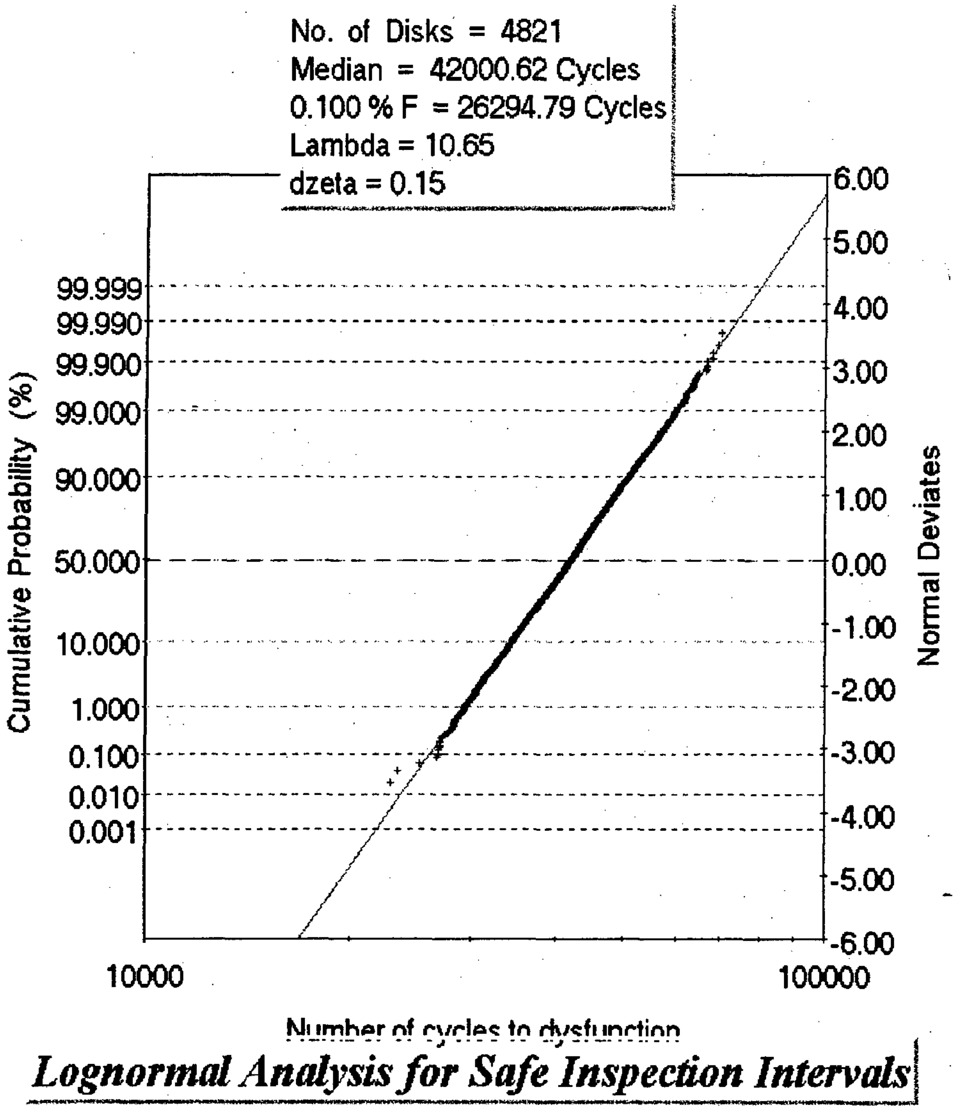

Figure 11. Lognormal analysis of PFM generated data for Nene-X turbine discs simulating the effect of worst possible scatter in FCGR data and uncertainties associated with the LPI technique.

Paper 14: Discussion

Question from P R Parolo – DGTA, Australia

How did you determine the Probability Of Detection (POD) for your eddy current inspection procedures?

Presenter's Reply

We utilised an extensive in-house database of POD on a wide variety of materials and crack data.

Question from Dr B Wicks – DSTO, Australia

In this case, you have looked at the critical location for crack initiation. How can you be assured that there is not another critical location for crack propagation that has a faster rate of crack growth than the one you have analysed?

Have you carried out this analysis for all critical locations on the disc?

Presenter's Reply

Spin rig testing was used to validate our assessment. Possible cracks initiating from other locations would be detected through NDI of the entire disc after rig testing.

Question from D Shepherd - QinetiQ, UK

You have put a lot of effort and time into developing a damage tolerance approach for this component. In view of the long crack-growth life available, would it not have been simpler and more cost-effective to implement a crack-growth based safe-life extension?

Presenter's Reply

Safe life is determined on the basis of a surface crack length of 0.8mm which, assuming that a surface crack length to crack depth ratio of 4 to 1 is observed in service induced cracks, corresponds to a crack depth of 200 microns. Assuming that most wrought disc materials possess an average grain size of 100 microns, the safe-life criteria then essentially considers time or cycles to metallurgical crack nucleation (between 1 and 3 grain diameters) in a disc. Crack-growth based safe-life extension is only possible if a crack, nucleated at the fracture critical location, is allowed to propagate beyond the 200 micron depth range to, say, 1000 microns, corresponding to 10 grain diameters. Within this range of crack depth, a typical disc material would be expected to exhibit short-crack growth behaviour, a phenomenon that is poorly understood at this time. Particular difficulties have been encountered in measuring short-crack growth rates and developing reliable empirical or metallurgical models for describing the short-crack growth behaviour. These limitations would impose unnecessary uncertainties on the predicted life-extension interval. It is, therefore, advisable to extend usable life on the basis of long-crack growth behaviour using damage tolerance concepts, since empirical as well as mechanistic models for describing the long-crack growth phenomenon, along with experimental techniques for measuring coupon and component level long-crack growth data are well established. In our opinion, crack-growth based safe-life extension should only be used in situations where it is not possible to implement a damage tolerance based safe inspection interval in the field.